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The heat-transfer coefficients for a hemisphere-cylinder model were determined experimentally using a transient technique. The model was designed to act as a calorimeter. The results are analyzed from the standpoint of accuracy, and a comparison with the theories of Sibulkin and Lees is made.

I. INTRODUCTION

Advances in aeronautical technology have created interest in the experimental determination of aerodynamic heating rates experienced by various shapes at supersonic and hypersonic Mach[1] numbers. Since June, 1960, the Jet Propulsion Laboratory (JPL) Aerodynamic Facilities Section has been conducting a test series in order to determine the quality of aerodynamic heating data that can be produced using the 21-in. hypersonic wind tunnel.

[1]See Nomenclature for a definition of terms.

The purpose of this Report is to present that portion of the results of this test series that pertains to the use of calorimetric models. Aerodynamic heating data are presented and compared to theories of Sibulkin and Lees. Discussions of the methods used, estimated accuracies, and inherent limitations are included.

The model used to obtain the results presented here was a 3-in. D hemisphere-cylinder 3.5-in. long. It was tested at Mach numbers 5.0 and 8.6 at angles of attack of 0, 6, and 15 deg.

II. FACILITIES AND EQUIPMENT

The JPL hypersonic wind tunnel is a continuous-flow facility having a Mach number capability of 4 through 11. Stagnation pressures of 650 psi and stagnation temperatures of 1300°F are attainable. A detailed description of the facility characteristics are given in Ref. 1. The special equipment required to test calorimetric models using the time-transient technique is described in Ref. 2. The data-recording equipment is described in Ref. 1.

III. MODEL DESCRIPTION

The model used for this portion of the test series was a 3-in. D hemisphere-cylinder 3.5-in. long, constructed from electrolytic nickel. The interior was hollow having a wall thickness which varied from 0.015 in. at the stagnation point of the hemisphere to 0.060 in. at the base of the cylinder. Both the interior and exterior surface of the model were polished.

The model was instrumented with 13 chromel-constantan thermocouples. The thermocouples were welded to the interior surface of the model on the vertical plane of symmetry. Figure 1 shows the thermocouple locations, and Fig. 2 shows the model installed in the tunnel.

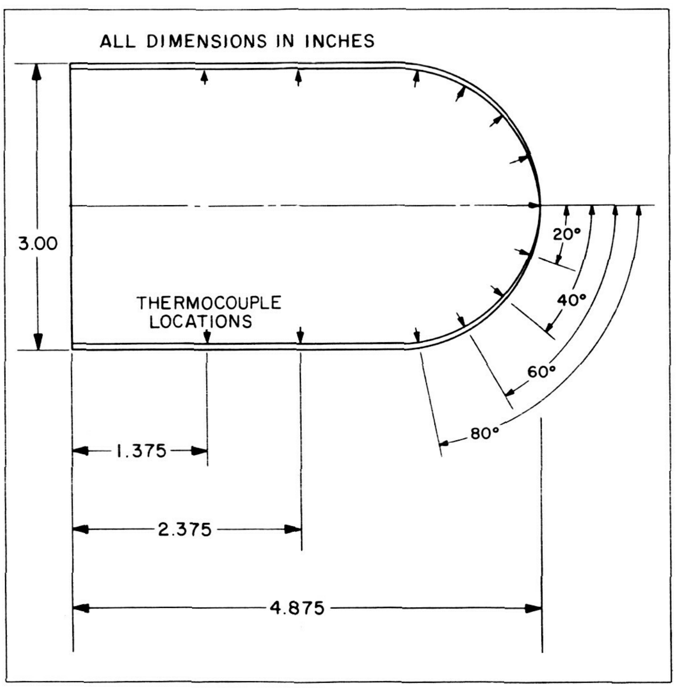
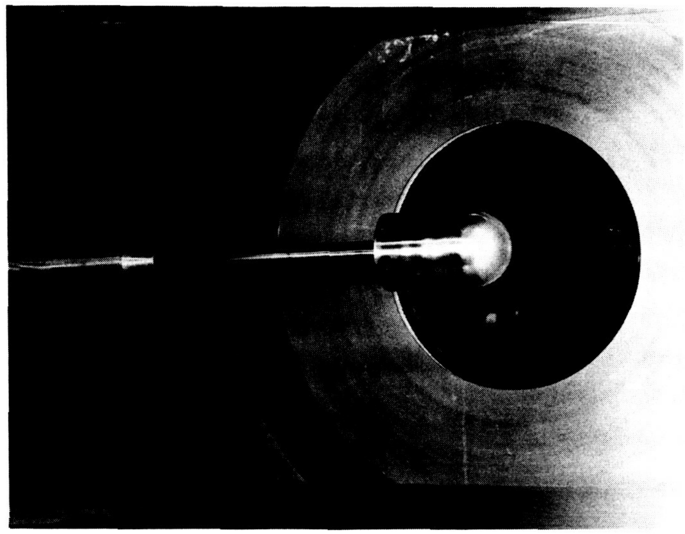
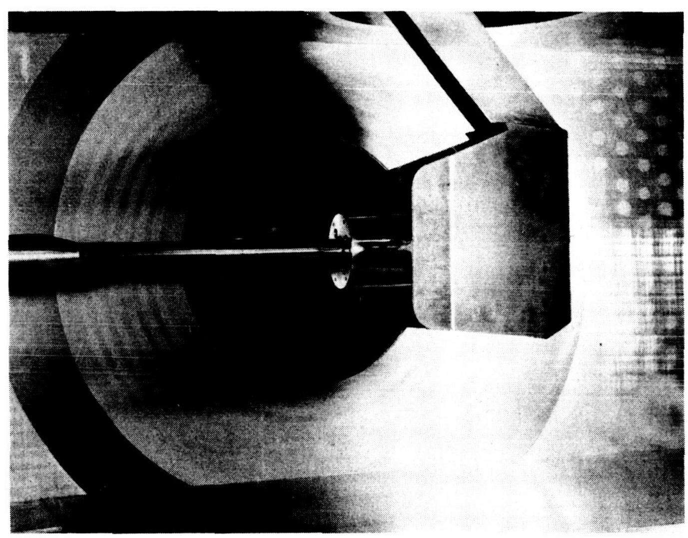


Fig. 1. Hemisphere-cylinder model showing thermocouple locations



a. Without heat-shield enclosure



b. With heat-shield enclosure

Fig. 2. Hemisphere-cylinder model installation

IV. DATA REDUCTION

The data-reduction procedures were tailored to the transient test procedure and equipment used for this portion of the test series. Although data recording was begun prior to retracting the shield, a 0.3-sec delay was allowed between the time the shield was energized and the data being recorded were considered valid. Approximately two-thirds of this time was required to allow the shield to pass clear of the model. The latter one-third of the period was used to minimize the effects of the thermal diffusivity characteristics of the model material on the measured model temperatures.

The basic equation used to reduce the measured temperature data to heat-transfer coefficient form is as follows:

$$h_m = \frac{wbc}{(T_t - T_m)} \left(\frac{\mathrm{d}\,T}{\mathrm{d}\,t}\right)_m \qquad (1)$$

It should be noted that the use of T_t rather than T_{aw} was an expedient in performing the data reduction.

The calculation of $(\mathrm{d}\,T/\mathrm{d}\,t)_m$ in Eq. 1 was made by finding the slope of a quadratic equation obtained from a least-squares curve fit to ½-sec intervals of temperature vs. time data. A calculation of $(\mathrm{d}\,T/\mathrm{d}\,t)_m$ (and subsequently h_m) was made for 6 succeeding ½-sec intervals from each test run.

The simplicity of Eq. 1 infers that both radiation to and conduction through the model skin is negligible. To conform with the latter of these requirements, the 6 succeeding calculations of h_m are used to determine what the value of h_m would have been at the time when the model was still isothermal, had a calculation been made then.

V. DISCUSSION

A. *Accuracy*

As was previously noted, the equation used to reduce the temperature data to values of the heat-transfer coefficient neglected any consideration of radiation to or from the model. This is valid, provided that the tunnel walls and the model are at approximately the same temperature. The tunnel walls are water-cooled while the tunnel is in use in order to maintain dimensional stability during operation. Tunnel wall temperatures are maintained between 60 and 120°F. During the tests described here, the model temperatures just prior to raising the shield ranged from 30 to 60°F from run to run. Thus, the radiation term was several orders of magnitude less significant than the convection term at the time the shield was raised.

The assumed negligibility of conduction through the model structure is not similarly justified, but is compensated in the following manner. Prior to raising the shield to begin a test run, the model is made as nearly isothermal as is possible. Attempts are made not to exceed thermal gradients of 1°F/in. on any part of the model. After the shield is raised, this condition no longer exists, but any continuous variations of the calculated heat-transfer coefficient with time must then be caused by errors induced by thermal conduction from other parts of the model. Thus, extrapolation of values of the heat-transfer coefficient to the time of minimum conduction gives the best value of the heat-transfer coefficient. The judicious selection of a model material of relatively low thermal conductivity permits the conduction term to be held to less than 1% of the convection term.

The certainty of the magnitude of the physical properties of the model material directly affects the accuracy of the results obtained as can be seen by inspection of the data-reduction equation. The obvious result is that only those materials of high quality composition are used. Electrolytic nickel was used for these tests, in part, because of its high purity. Some types of stainless steel are also acceptable. In addition to material properties, the dimensional properties of the model affect the quality of the results. The model thickness at each thermocouple location was measured several times using various inspection methods. The total error caused by uncertainties in the values of specific weight, specific heat, and skin thickness is estimated not to exceed ±4%, but, in general, appears to be much better than that.

Considerable work has been done to evaluate the characteristics of the equipment used to record the temperature data for these tests. The results of this evaluation have shown that the recorded data are subject to errors which are random both in magnitude and time of occurrence, never exceeding a value of ±0.017 mv. This random noise is superimposed on the thermocouple signals.

The numerical-analysis technique used to obtain the temperature data from the thermocouple signals is based on the knowledge of the quality of the recorded data. Because the noise is random, sufficient justification exists in using a periodic method of determining the temperature vs. time curve slope. This assures that a systematic error has not been inadvertently included in the numerical analysis. The numerical-analysis technique used for these tests represented a least-squares curve fit over data obtained during ½-sec (11 data points) intervals where the solution gave a best value of dT/dt at the midpoint of the time period. The selection of the ½-sec interval was a somewhat arbitrary choice in that a longer time period would have provided stronger smoothing but would have required a longer extrapolation of values of h back to the time of shield retraction. The accuracy of the calculated values of dT/dt with respect to the true values of dT/dt being experienced by the model was a function of the thermocouple calibration; for the chromel-constantan thermocouple used for these tests, the maximum error was approximately ±25°F/sec. This represents a ±13% error for the highest heating rates and a ±50% error for the lowest heating rates experienced for these tests. By manual inspection of the calculated data, it was possible selectively to eliminate data having near maximum errors by simultaneously evaluating the results obtained from several adjacent thermocouples. This was done for all data except for those approaching the ±50% error region described above. It is estimated that this selective technique reduced the errors to 33% of the values quoted above, except in the high error region where no improvement could be made.

The accuracy of the measurement of the stagnation temperature of the free-stream and the model temperature has been established as ±5°F; therefore, the maximum error in $(T_t - T_m)$ is approximately ±10°F. For these tests, the percentage inaccuracies contributed by uncertainties in the temperature measurements are ±1% at the higher stagnation temperatures and ±2% at the lower stagnation temperatures.

Considering all of the above items which represent all of the significant sources of error contribution, the following total-error analysis results. For data where the heating creates temperature changes in excess of 75°F/sec, the maximum possible error is equivalent to the sum of all of the errors quoted above. That is,

$$\begin{aligned}\epsilon_h &= \text{conduction error} + \text{physical properties error} \\ &\quad + \text{temp. slope error} + \text{temp. error} \\ &= 1 + 4 + 4.3 + 2 = 11.3\% \end{aligned} \tag{2}$$

or assuming the likelihood of compensating errors, the root-sum-squared error is

$$\epsilon_{h_{rss}} \cong \sqrt{1 + 16 + 18.5 + 4} = 6.3\% \tag{3}$$

For data where the heating creates temperature changes of 40°F/sec, the errors are approximately

$$\epsilon_h \cong 1 + 4 + 50 + 2 = 57\% \tag{4}$$

or

$$\epsilon_{h_{rss}} \cong \sqrt{1 + 16 + 2500 + 4} = 50.2\% \tag{5}$$

For data where the heating creates temperature changes less than 40°F/sec, the errors tend to become indeterminate because both the error terms for conduction and the temperature slope become excessively large. Errors in excess of several hundred percent have been observed.

B. Analysis

Theoretical values of the heat-transfer coefficient were determined for the vertical meridian of the model shape using the theories of Sibulkin (Ref. 3) and Lees (Ref. 4), adjusting the values to compensate for the difference between T_{aw} and T_t. The stagnation-point pressure gradients required to determine the stagnation-point heat-transfer coefficient were calculated using Newtonian impact theory. The experimental results were then compared to the theoretical curve. These comparisons appear in Fig. 3. It should be noted that the heat-transfer coefficient is plotted vs. the linear S/D ratio as measured from the theoretical stagnation point as it varies with angle of attack.

The theoretical and experimental results compare very well in the region between the values of S/D of 0.3 to

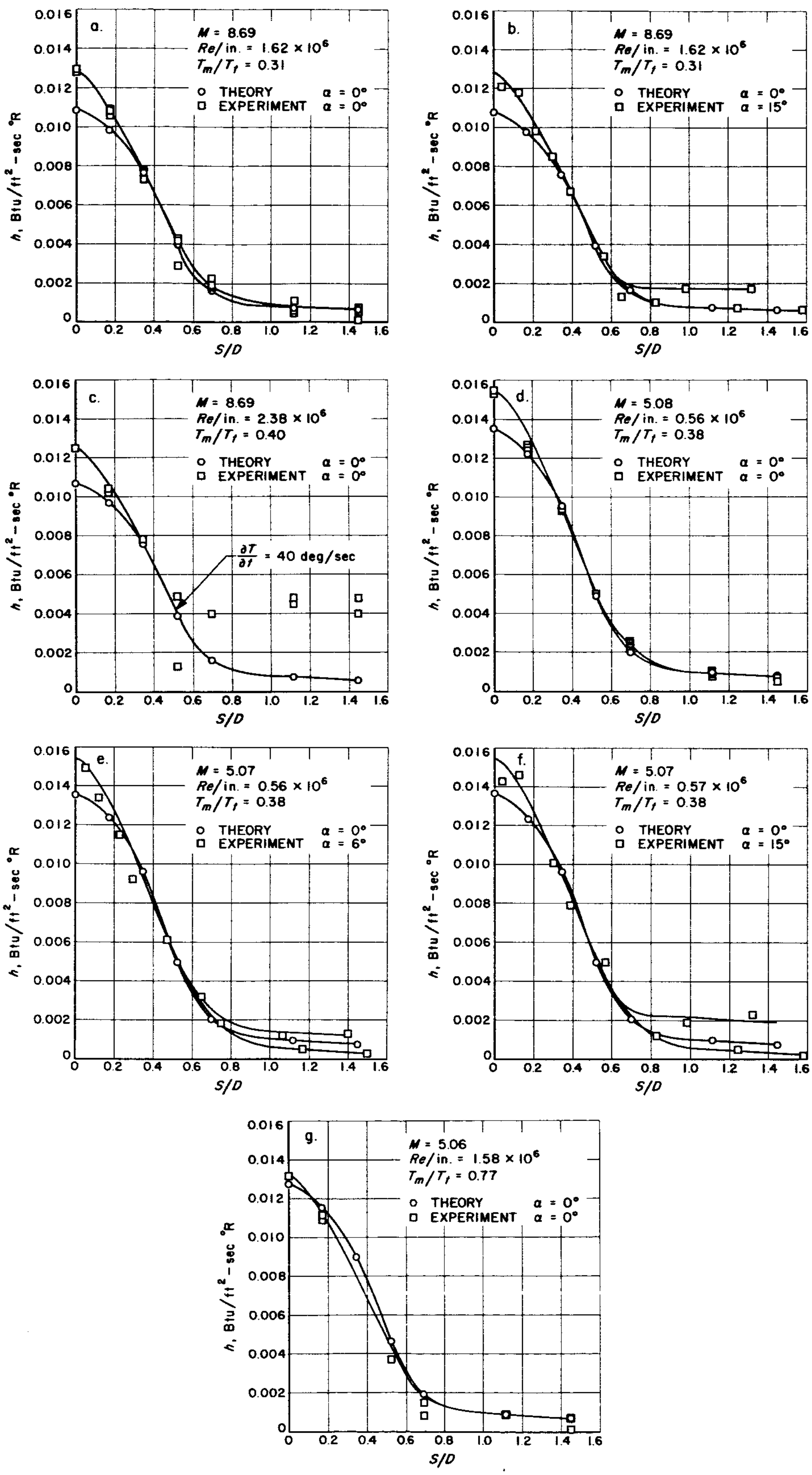


Fig. 3. Heat-transfer coefficient as a function of the linear surface distance/diameter ratio

0.75, regardless of the angle of attack. In the cases where the model is at the angle of attack, the data obtained on the lee side of the cylinder agree very well; on the windward side, the experimental results show an expected increase over the zero angle-of-attack results. The theoretical and experimental stagnation point heat-transfer coefficient values differ by approximately 10% at all conditions tested except at Mach number 5, $T_m/T_t = 0.78$. The experimental values of the heat-transfer coefficient decrease rapidly away from the stagnation point as an approximate function of the $\cos^3 \theta$ to a value of θ of about 25° ($S/D \cong 0.22$).

C. Operation

The potential accuracies of transient heat-transfer results can be improved by judicious selection of the model dimensions and materials and the thermocouple material. The wall thickness of the model should be thin enough to afford a significant temperature change rate, but not so thin that thickness measurement uncertainties contribute significant uncertainty in the final results. The model material should be selected for both structural strength and good thermal properties. Good thermal properties are represented by relatively low specific heat and thermal conductivity and a relatively high thermal diffusivity. These conditions will provide a relatively rapid temperature rise with a minimum thermal lag. Some of the several types of stainless steels are better, thermally, than the nickel used during these tests but are more difficult to work. The thermocouple type should be selected so that the signal change rate generated by the thermal change rate is distinguishable from the instrumentation noise. Chromel-constantan thermocouples, used successfully during these tests, have the highest signal/temperature ratio of all the common base metal thermocouples.

The selection of "time-zero," the time to which all values of heat-transfer coefficient are extrapolated, is somewhat arbitrary. Because it was suspected that this time was not necessarily the time of shield retraction, several tests were conducted using analog equipment to record the thermocouple signals. The results of these tests were that the best time-zero was that time when the data were first considered valid. For these tests a 0.3-sec delay was used between the time of shield firing and time-zero.

An operational difficulty, encountered at low stagnation pressures at Mach 5, was the apparent temporary loss of supersonic flow. This occurred when the cooling shield was extracted. On several occasions the loss of flow was noted and the data discarded. Analysis of the results of some tests considered valid shows that the model was exposed to subsonic flow during the time the data were being recorded, supersonic flow having been re-established, unnoticed.

VI. CONCLUSIONS

The transient technique used at JPL to determine the aerodynamic heating of calorimetric models in the hypersonic wind tunnel is a valid testing method. The quadratic least-squares curve fit, the selection of time-zero, and the extrapolation of the heat-transfer values back to time-zero provide a convenient and reliable method of reducing the collected data to useful results.

The accuracy of the reduced data depends upon the rate of change of the temperature of the model. When the temperature change rate exceeds 75°F/sec, the heat-transfer errors will not generally exceed 7% of the true value. When the temperature change rate is on the order of 40°F/sec, the heat-transfer errors can be greater than 50% of the true value. The errors, when the temperature change rate is less than 40°F/sec, are indeterminate but very large.

The comparisons of experimental results with the theories of Sibulkin and Lees show good agreement when S/D is greater than 0.3. The stagnation-point heat-transfer theory of Sibulkin underestimates the experimental stagnation point results by approximately 10% when the model-to-stagnation temperature ratio is on the order of 0.4.

The highest quality heat-transfer results can be assured by the proper selection of model thickness and material. Model walls should be as thin as possible, commensurate with the manufacturing facilities' ability to measure the thickness accurately after manufacture. Model materials having low specific heat and high thermal diffusivity are desirable.

Care must be taken to assure that supersonic flow is not temporarily lost when the cooling shield is retracted. Loss of supersonic flow becomes most probable at relatively low stagnation pressures because the available compression ratios become marginal in the hypersonic tunnel.

NOMENCLATURE

b	model wall thickness, ft
c	model-material specific heat, Btu/16
D	model base diameter, in.
h	heat-transfer coefficient, Btu/ft^2-sec °R
M	Mach number
Re/in.	Reynolds number per inch, 1/in.
S	surface distance from theoretical stagnation point, in.
T	temperature, °R
t	time, sec
w	model material density, lb/ft^3
α	angle of attack, deg
θ	angle between free-stream velocity vector and plane of tangency at point of interest, deg

Subscripts

aw	adiabatic wall
t	stagnation
m	model

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